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ELASTIC-PLASTIC ANALYSIS OF A STEEL PRESSURE VESSEL WRAPPED WITH MULTILAYERED COMPOSITES

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13. ABSTRACT *(Maximum 200 words)*
An elastic-plastic analysis of stresses and strains in an internally pressurized, composite-jacketed cylinder is studied here. Each layer is orthotropic but with different material properties. Analytical expressions are derived for a steel pressure vessel wrapped with multilayered composites. Numerical results are obtained for three types of composite jackets. The interface pressure, hoop strains, and stresses in the liner and jacket are presented.

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INTRODUCTION

In recent years there has been increasing emphasis on the use of composite materials in armament structures. A current problem in Army cannon design is to replace a portion of the steel wall thickness with a lighter material. The inner portion, steel liner, maintains the tube projectile interface and shields the composite from the extremely hot gases. The outer portion, composite jacket, is made of single or multilayered graphite-bismaleimide wound and wrapped on the steel liner. Two subscale models have been fabricated and tested (refs 1,2). An analytical elastic-plastic solution for the model with a single-layered composite jacket has been presented in a recent report (ref 3). This report covers an elastic-plastic analysis for the model with a multilayered composite jacket. Analytical solutions are presented separately for the composite-jacket and steel liner and then for the compound cylinder problem. Numerical results are obtained for loading within and beyond the elastic region up to failure.

COMPOSITE JACKET

The composite jacket is made of n layers bounded by radii $(r_1, r_2, \ldots, r_n, r_{n+1})$. Each layer is elastically orthotropic but with different material properties. The strain-stress relations for the k-th layer in cylindrical coordinates are given by

$$\begin{bmatrix} \epsilon_r^{(k)} \\ \epsilon_\theta^{(k)} \\ \epsilon_z^{(k)} \end{bmatrix} = \begin{bmatrix} 1/E_r & , -\nu_{\theta r}/E_\theta & -\nu_{zr}/E_z \\ -\nu_{r\theta}/E_r & , 1/E_\theta & -\nu_{z\theta}/E_\theta \\ -\nu_{rz}/E_r & , -\nu_{\theta z}/E_\theta & 1/E_z \end{bmatrix}^{(k)} \begin{bmatrix} \sigma_r^{(k)} \\ \sigma_\theta^{(k)} \\ \sigma_z^{(k)} \end{bmatrix} \tag{1}$$

or

$$\epsilon_i^{(k)} = S_{ij}^{(k)} \sigma_j^{(k)} \quad (i,j = r,\theta,z) \tag{2}$$

where $S_{ij}^{(k)}$ are components of the compliance matrix. The superscript k refers to the k-th layer. In plane-strain conditions, the above strain-stress relations modify to

$$\begin{bmatrix} \epsilon_r^{(k)} \\ \epsilon_\theta^{(k)} \end{bmatrix} = \begin{bmatrix} \beta_{rr}^{(k)} & \beta_{r\theta}^{(k)} \\ \beta_{r\theta}^{(k)} & \beta_{\theta\theta}^{(k)} \end{bmatrix} \begin{bmatrix} \sigma_r^{(k)} \\ \sigma_\theta^{(k)} \end{bmatrix} \tag{3}$$

where

$$\begin{aligned} \beta_{rr}^{(k)} &= (1-\nu_{rz}^{(k)}\nu_{zr}^{(k)})/E_r^{(k)} \\ \beta_{r\theta}^{(k)} &= -(\nu_{\theta r}^{(k)}+\nu_{\theta z}^{(k)}\nu_{zr}^{(k)})/E_\theta^{(k)} \\ \beta_{\theta\theta}^{(k)} &= (1-\nu_{\theta z}^{(k)}\nu_{z\theta}^{(k)})/E_\theta^{(k)} \end{aligned} \tag{4}$$

The normal traction acting on the interface between (k-1)th and k-th layers is denoted by q_k. Then the general elastic solution for the k-th layer bounded by radii (r_k, r_{k+1}) and subjected to interface pressure (q_k, q_{k+1}) is given by (ref 4)

$$\begin{aligned} \sigma_r^{(k)} &= (-a_k q_k + c_k q_{k+1})(r_{k+1}/r)^{g_k+1} + (a_k q_k - b_k q_{k+1})(r/r_{k+1})^{g_k-1} \\ \sigma_\theta^{(k)} &= g_k(a_k q_k - c_k q_{k+1})(r_{k+1}/r)^{g_k+1} + g_k(a_k q_k - b_k q_{k+1})(r/r_{k+1})^{g_k-1} \\ u^{(k)} &= r(\beta_{r\theta}^{(k)}\sigma_r^{(k)} + \beta_{\theta\theta}^{(k)}\sigma_\theta^{(k)}) \end{aligned} \tag{5}$$

where

$$\begin{aligned} d_k = r_{k+1}/r_k \quad &, \quad g_k = (\beta_{rr}^{(k)}/\beta_{\theta\theta}^{(k)})^{1/2} \\ c_k = (d_k^{2g_k}-1)^{-1} \quad , \quad b_k &= c_k d_k^{2g_k} \quad , \quad a_k = c_k d_k^{g_k-1} \end{aligned} \tag{6}$$

At the two ends of the k-th layer, the expressions for the displacements and hoop stresses are

$$\begin{aligned} u_{k+1} &= (A_k q_k - B_k q_{k+1}) r_{k+1} \\ u_k &= (C_k q_k - D_k q_{k+1}) r_k \\ \sigma_\theta^{(k)} &= 2a_k g_k q_k - (b_k + c_k) g_k q_{k+1} \quad \text{at } r_{k+1} \\ \sigma_\theta^{(k)} &= (b_k + c_k) g_k q_k - 2a_k d_k^2 g_k q_{k+1} \quad \text{at } r_k \end{aligned}$$

where

$$A_k = 2a_k q_k \beta_{\theta\theta}^{(k)} \quad , \quad B_k = \beta_{r\theta}^{(k)} + (b_k+c_k) q_k \beta_{\theta\theta}^{(k)}$$

$$C_k = -\beta_{r\theta}^{(k)} + (b_k+c_k) q_k \beta_{\theta\theta}^{(k)} \quad , \quad D_k = 2a_k d_k^2 q_k \beta_{\theta\theta}^{(k)} \tag{8}$$

At the interfaces ($r_k, k=2,\ldots,n$), the displacements should be continuous and these require

$$A_{k-1} q_{k-1} - B_{k-1} q_k = C_k q_k - D_k q_{k+1} \tag{9}$$

Let $\bar{Q}_k = q_k/q_n$ for all k, then $\bar{Q}_{n+1} = 0$, $\bar{Q}_n = 1$, and we can calculate $\bar{Q}_{k-1}$ backward for k = n to 2 by

$$\bar{Q}_{k-1} = A_{k-1}^{-1}[(B_{k-1}+C_k)\bar{Q}_k - D_k\bar{Q}_{k+1}]$$

Normalizing by $\bar{Q}_1$ leads to

$$\bar{q}_k = q_k/q_1 \quad \text{for } k = 1,2,\ldots,n \tag{10}$$

i.e., the relative values for the interface pressures when $q_1 = 1$. We can also obtain the corresponding displacements $\bar{u}_1,\ldots,\bar{u}_n$, $\bar{u}_{n+1}$ at $r_1,\ldots,r_n,r_{n+1}$.

STEEL LINER

The steel liner of inside radius a and outer radius b is elastic-plastically isotropic and assumed to obey Tresca's yield criterion, the associated flow rule, and linear strain-hardening. The elastic solution for the steel liner subjected to internal pressure p and external pressure q is

$$\begin{matrix}\sigma_r \\ \sigma_\theta\end{matrix} = \{\mp(p-q)(b/r)^2 + p-q\, b^2/a^2\}/(b^2/a^2-1)$$

$$u/r = E^{-1}(1+\nu)[(p-q)(b/r)^2 + (1-2\nu)(p-q\, b^2/a^2)]/(b^2/a^2-1) \tag{11}$$

When the internal pressure p is large enough, part of the steel liner ($a \leq r \leq \bar{\rho}$) will become plastic and ρ is the elastic-plastic interface. The elastic-plastic solution can be written in the elastic portion ($\rho \leq r \leq b$) as

$$\frac{E}{\sigma_o}\frac{u}{r} = \frac{1+\nu}{2}\frac{\rho^2}{r^2} + (1-\nu-2\nu^2)\left[\frac{1}{2}\frac{\rho^2}{b^2} - \frac{q}{\sigma_o}\right]$$

$$\left.\begin{matrix}\sigma_r/\sigma_o \\ \sigma_\theta/\sigma_o\end{matrix}\right. = \frac{1}{2}\left(\mp\frac{\rho^2}{r^2} + \frac{\rho^2}{b^2}\right) - \frac{q}{\sigma_o}$$

$$\sigma_z/\sigma_o = \nu\ \rho^2/b^2 - 2\nu\ q/\sigma_o \qquad (12)$$

and in the plastic portion ($a \leq r \leq \rho$)

$$\frac{E}{\sigma_o}\frac{u}{r} = (1-\nu-2\nu^2)\frac{\sigma_r}{\sigma_o} + (1-\nu^2)\frac{\rho^2}{r^2}$$

$$\left.\begin{matrix}\sigma_r/\sigma_o \\ \sigma_\theta/\sigma_o\end{matrix}\right. = \mp\frac{1}{2}\left(1-\eta\beta+\eta\beta\frac{\rho^2}{r^2}\right) + \frac{1}{2}\frac{\rho^2}{b^2} - (1-\eta\beta)\ln\frac{\rho}{r} - \frac{q}{\sigma_o}$$

$$\sigma_z/\sigma_o = \nu\ \rho^2/b^2 - 2\nu(1-\eta\beta)\ln\frac{\rho}{r} - 2\nu\ q/\sigma_o$$

$$\bar{\epsilon}^p = \beta(\rho^2/r^2-1)\quad ,\quad \eta\beta = \frac{m}{m + \frac{3}{4}\frac{(1-m)}{(1-\nu^2)}}$$

$$\eta = \frac{2}{\sqrt{3}}\frac{E}{\sigma_o}\frac{m}{1-m}\quad ,\quad m = \frac{E_t}{E}\quad ,\quad \sigma = \sigma_o(1+\eta\bar{\epsilon}^p) \qquad (13)$$

where σ_o is the initial tensile yield stress and E_t is the tangent modulus in the plastic range of the stress-strain curve.

When the internal pressure is further increased, the steel liner will become fully-plastic. Using Tresca's yield criterion, the associated flow rule, and assuming linear strain-hardening, the fully-plastic solution derived in Reference 3 is given below.

Subject to $\sigma_\theta \geq \sigma_z \geq \sigma_r$, the analytical expressions for the stresses and displacement are

$$\sigma_r = -p + \sigma_o(1-\eta\beta)\ln\left(\frac{r}{a}\right) + \frac{1}{2}\frac{\eta\beta}{(1-\nu^2)}\left[\frac{b^2}{a^2} - \frac{b^2}{r^2}\right]E\Phi$$

$$\sigma_\theta = \sigma_r + \sigma_o(1+\eta\bar{\epsilon}^p)$$

$$ru = E^{-1}(1-2\nu)(1+\nu)r^2\sigma_r + \Phi\ b^2 \qquad (14)$$

where

$$\phi = u_b/b + (1-2\nu)(1+\nu)E^{-1}q$$

$$\bar{\epsilon}^p = \frac{2}{\sqrt{3}} [\phi\, b^2/r^2 - (1-\nu^2)\sigma_o/E]/[1 + \frac{2}{\sqrt{3}} (1-\nu^2)\eta\sigma_o/E]$$

COMPOUND CYLINDER

The compound cylinder consists of an inner steel liner and an outer composite jacket. The steel liner of inside radius a and outer radius b is wrapped by a multilayered composite jacket. The displacement and normal traction at the interface between the liner and jacket should be continuous, i.e., $q = q_1$ and $u_b = u_1$. From these conditions we can determine the relations between p and q.

When the internal pressure p is small, an explicit functional relation exists

$$\frac{2p}{q} = \frac{(b^2/a^2-1)}{(1-\nu^2)} [E(C_1-D_1\bar{q}_2) + (1-\nu-2\nu^2)] + 2 \tag{15}$$

where every term in the right-hand side is known. The displacement at the bore can also be expressed as an explicit function of p

$$(\frac{b^2}{a^2} - 1) \frac{E}{p} \frac{u_a}{a} = (1+\nu) \frac{b^2}{a^2} + (1-\nu-2\nu^2) - 2(1-\nu^2) \frac{b^2}{a^2} \frac{q}{p} \tag{16}$$

When the internal pressure is large enough, part of the steel liner will become plastic. The elastic-plastic solution is given in terms of the parameter ρ. The conditions of continuity require

$$\frac{q}{\sigma_o} = \frac{(1-\nu^2)\rho^2/b^2}{(1-\nu-2\nu^2) + E(C_1-D_1\bar{q}_2)} \tag{17}$$

This, together with

$$\frac{p}{\sigma_o} = \frac{q}{\sigma_o} + \frac{1}{2}(1- \frac{\rho^2}{b^2}) + (1-\eta\beta)\ln \frac{\rho}{a} + \frac{\eta\beta}{2} (\frac{\rho^2}{a^2} - 1) \tag{18}$$

serves to give an implicit relation between p and q. By letting $\rho = a$ and b, we can determine the lower limits p^*, q^*, u_a^*, u_b^* and the upper limits p^{**}, q^{**}, u_a^{**}, u_b^{**}, respectively.

When the internal pressure p is further increased, i.e., $p > p^{**}$, $u_a > u_a^{**}$, $u_b > u_b^{**}$, the conditions of continuity lead to

$$\Phi = q[(C_1 - D_1\bar{q}_2) + (1-\nu-2\nu^2)/E] \tag{19}$$

and

$$\frac{p}{\sigma_o} = (1-\eta\beta)\ln\frac{b}{a} + \frac{q}{\sigma_o}\left\{1 + \frac{\eta\beta(b^2/a^2-1)}{2(1-\nu^2)}[E(C_1-D_1\bar{q}_2) + (1-\nu-2\nu^2)]\right\} \tag{20}$$

It should be pointed out that the pressure q and the displacement u_b at the interface are linear functions of internal pressure p. The bore displacement u_a can be written as

$$\frac{u_a}{a} = -(1-2\nu)(1+\nu)\frac{p}{E} + \frac{b^2}{a^2}\Phi \tag{21}$$

which is also a linear function of internal pressure p.

NUMERICAL RESULTS

Given any value of internal pressure, we can obtain numerical results for the stresses and strains in the radial and tangential directions and also for the displacement at any radial position in a steel pressure vessel wrapped with multilayered composites. The steel liner for the subscale test specimens (ref 1) had an inner diameter of 2.0 inches and an outer diameter of 2.34 inches. The steel was 4130 seamless mechanical tubing heat treated to a hardness of 34 to 36 Rockwell "C." A standard ASTM tensile test was conducted to determine the 0.1 percent-offset yield strength (120 Ksi) and the ultimate tensile strength (140 Ksi). The composite jacket is a graphite-bismaleimide produced by Fiberite Corporation. Its cure temperature is 450°F and it is wound and wrapped on the steel liner in the same manner as the full-scale gun tube

specimen denoted as CTL III. The layup is again approximately half-scale and made up of two longitudinal layers alternating with two circumferential layers. Sixteen layers are applied in this way. Lamina properties for this material are given in Table I. For the purpose of comparison, numerical results are obtained for four types of composite jackets as shown in Table II. Cases 3 and 4 represent four hoop-axial and axial-hoop alternating layers, respectively, while cases 1 and 2 represent eight axial and hoop layers, respectively. The total thickness of each composite jacket is 0.12 inch, and the steel liner is assumed to be linear strain-hardening with a = 1 inch, b = 1.17 inches, σ_0 = 120 Ksi, m = 0.04. In addition to the lower and upper limits (p^* and p^{**}) of internal pressure in the elastic-plastic range, we also show in Table II two other limits ($P_{0.8}$ and $P_{1.3}$) which correspond to the internal pressure when u_b/b = 0.8 and 1.3 percent, respectively. It should be noted that u_b/b is the maximum hoop strain in the composite. Brittle failure of the composite material is assumed to occur at a maximum strain of 0.8 or 1.3 percent. The limits ($P_{0.8}$ or $P_{1.3}$) will be the maximum values of internal pressure these compound tubes can contain without failure.

TABLE I. ELASTIC CONSTANTS OF STEEL AND COMPOSITE MATERIALS

Material	E_θ x10^6 psi	E_r x10^6 psi	E_z x10^6 psi	ν_{rz}	$\nu_{r\theta}$	$\nu_{z\theta}$
Hoop lamina Im6	21.0	1.0	1.0	0.40	0.02	0.02
Axial lamina G50	1.3	1.3	31.0	0.01	0.39	0.39
Steel 4130	30.8	30.8	30.8	0.30	0.30	0.30

TABLE II. LIMITS OF INTERNAL PRESSURE FOR FOUR CASES

Case	Layup	p*	p**	$p_{0.8}$	$p_{1.3}$
1	$(90°)_8$	16.49	19.44	21.26	23.34
2	$(0°)_8$	20.95	25.55	35.20	45.99
3	$(0°,90°)_4$	18.87	22.70	28.59	35.25
4	$(90°,0°)_4$	18.80	22.60	28.38	34.90

The pressure at the interface between the liner and jacket has been obtained as a function of internal pressure and the results for the first three cases are shown in Figure 1. The results of the hoop strains at the bore, interface between the liner and jacket, and outside surface for three cases are shown in Figures 2, 3, and 4 respectively, as functions of internal pressure. The complete (including elastic, elastic-plastic, and fully-plastic) ranges of loadings up to $p_{0.8}$ have been considered. These numerical results for the strains are presented here for future comparisons with experimental results. The results of hoop stresses in the liner at the bore are shown in Figure 5 as functions of internal pressure. It should be noted that the relation changes drastically when yielding occurs. The results of hoop stresses in the liner at the interface are shown in Figure 6 as functions of internal pressure. The relation changes from linear to nonlinear when yielding sets in and more significant change occurs when the fully-plastic state is reached. The distribution of hoop stresses in the liner and jacket can be obtained at any given value of internal pressure. In Figures 7, 8, and 9 we present the numerical results for three cases of composite jackets at three values of internal pressure, i.e., $p = p^*$, p^{**} and when half of the liner is plastic. The values of internal pressure when half of the liner is plastic are p = 18.61, 23.86, 21.41 Ksi for

cases 1. 2. 3. respectively. The values of two limits, p* and p**, are given in Table II for all four cases. When the composite jacket is made of axial lamina only, the hoop stresses in the jacket are very small as shown in Figure 7. When the liner is wrapped by hoop lamina only, the hoop stresses in the jacket become larger as the internal pressure increases as shown in Figure 8. When the jacket consists of alternating hoop-axial lamina, the hoop stresses become discontinuous not only at the interface between the liner and jacket but also at all other interfaces between axial and hoop lamina.

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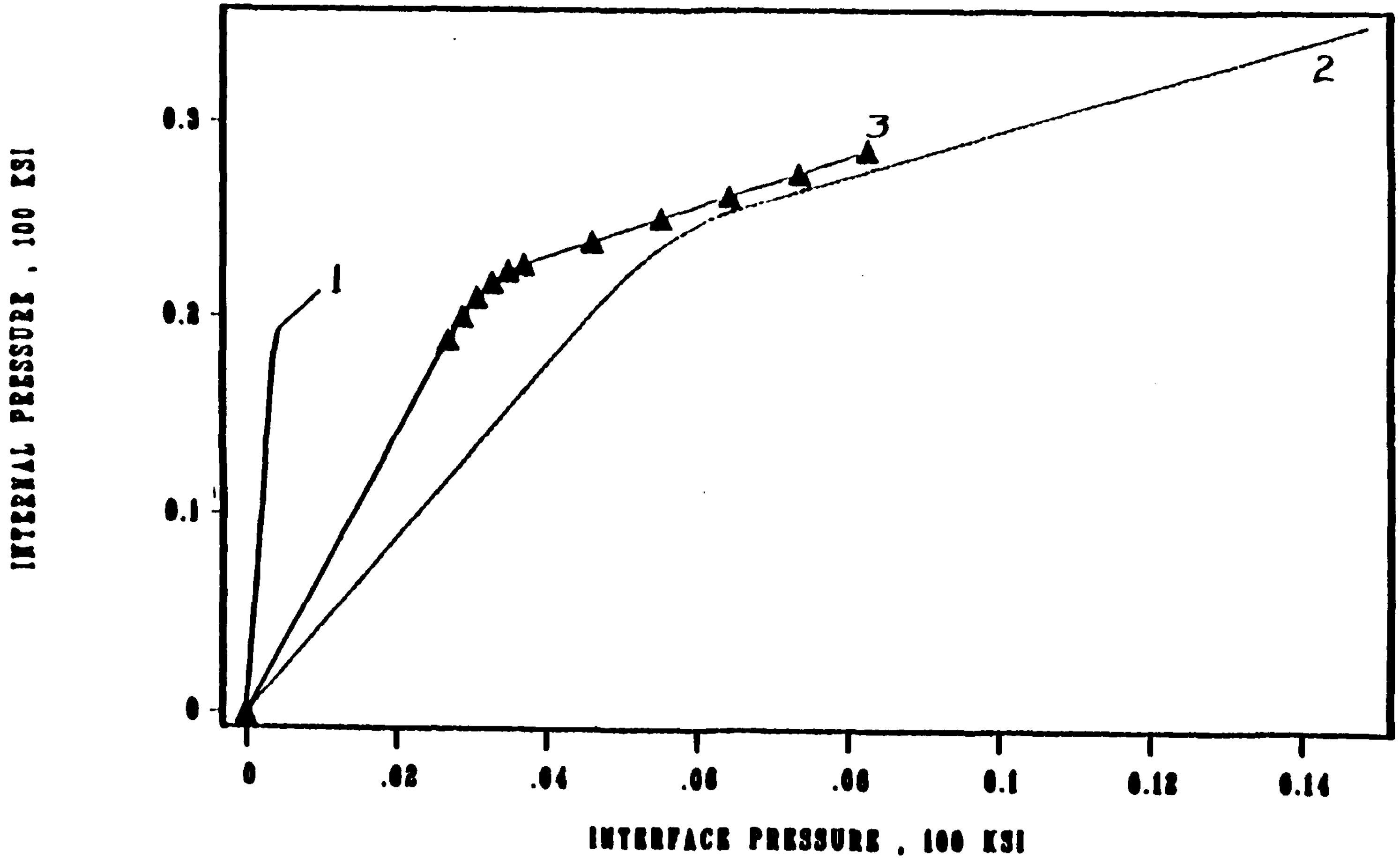

Figure 1. Interface pressure as a function of internal pressure.

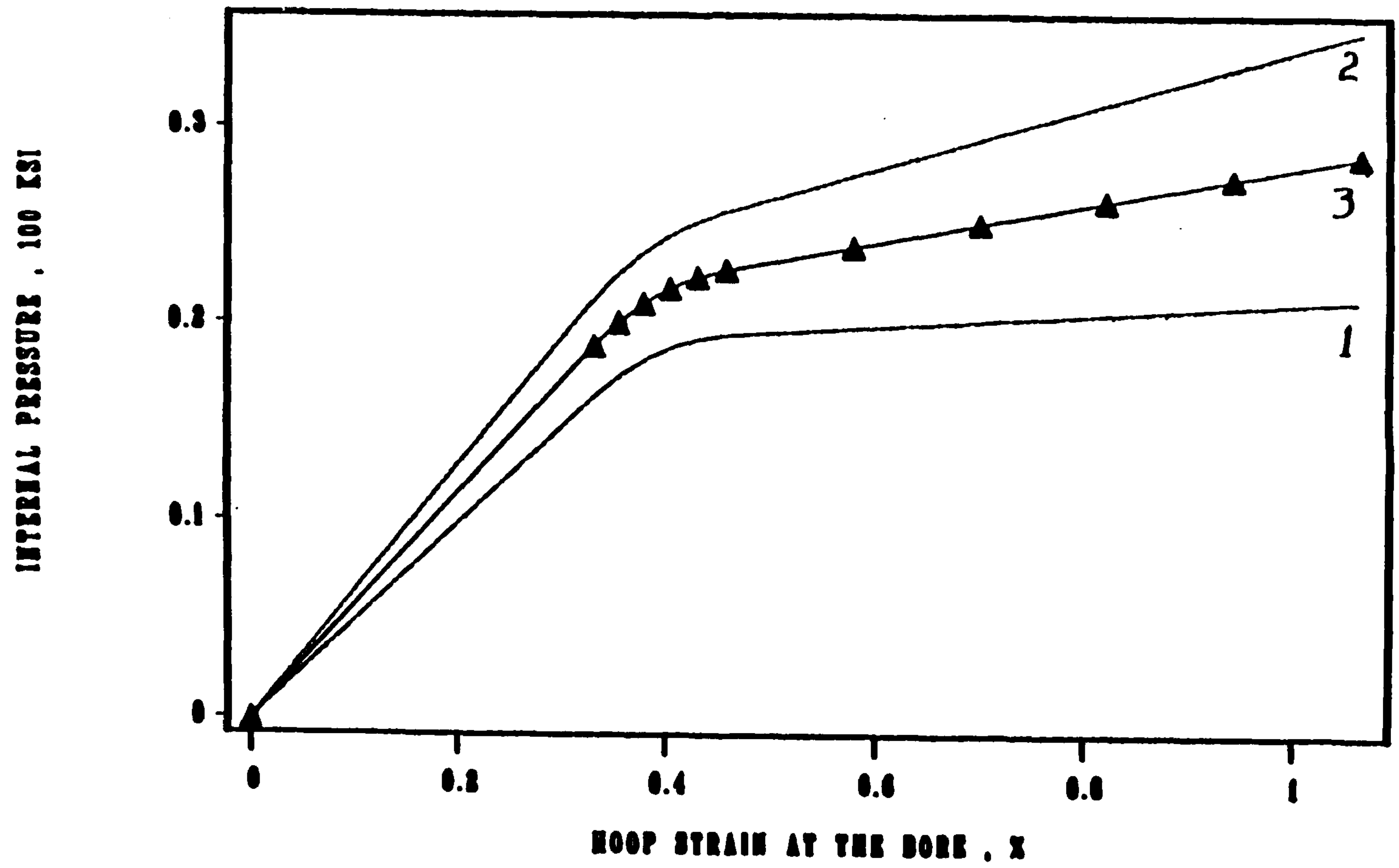

Figure 2. Hoop strain at the bore as a function of internal pressure.

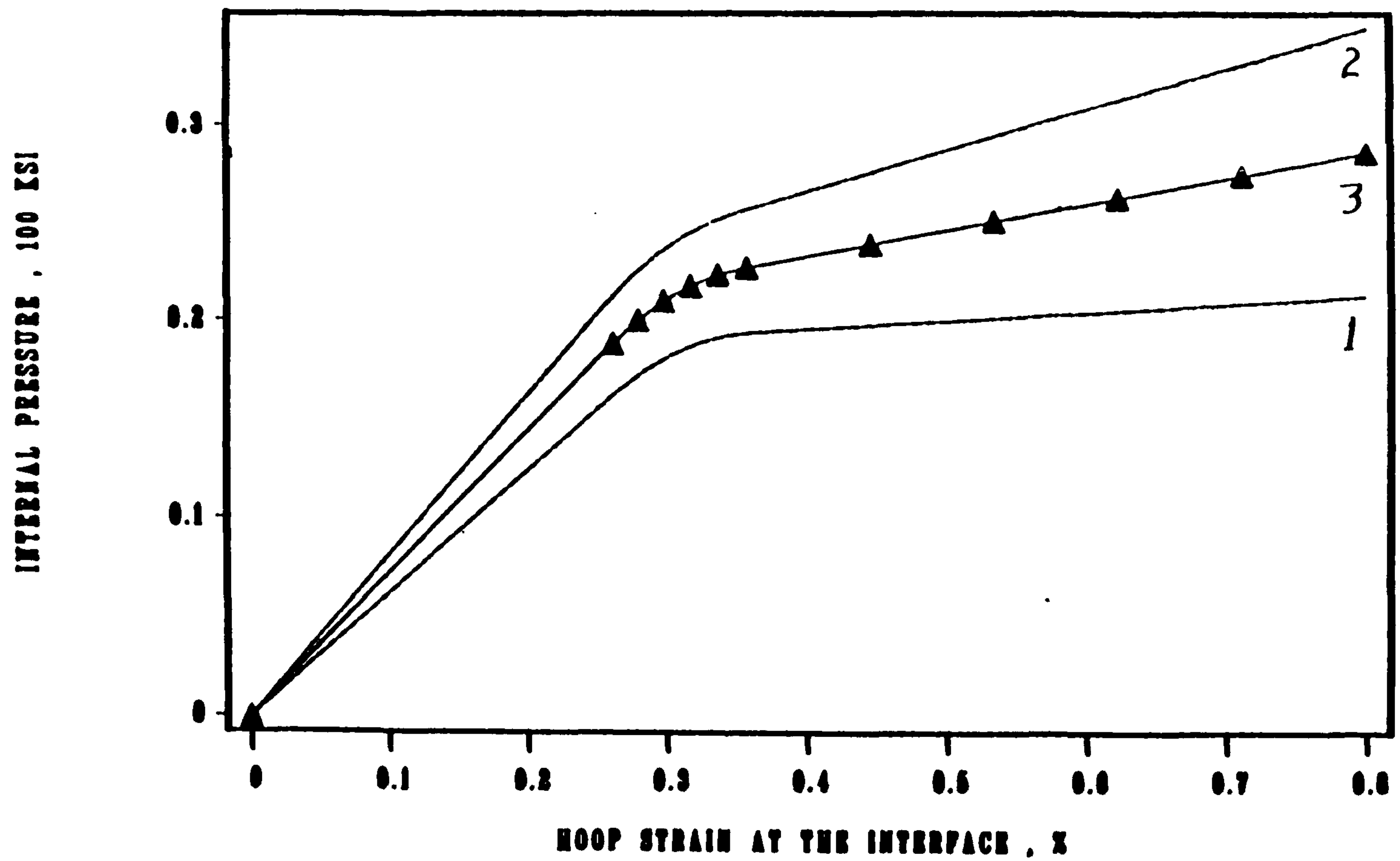

Figure 3. Hoop strain at the interface as a function of internal pressure.

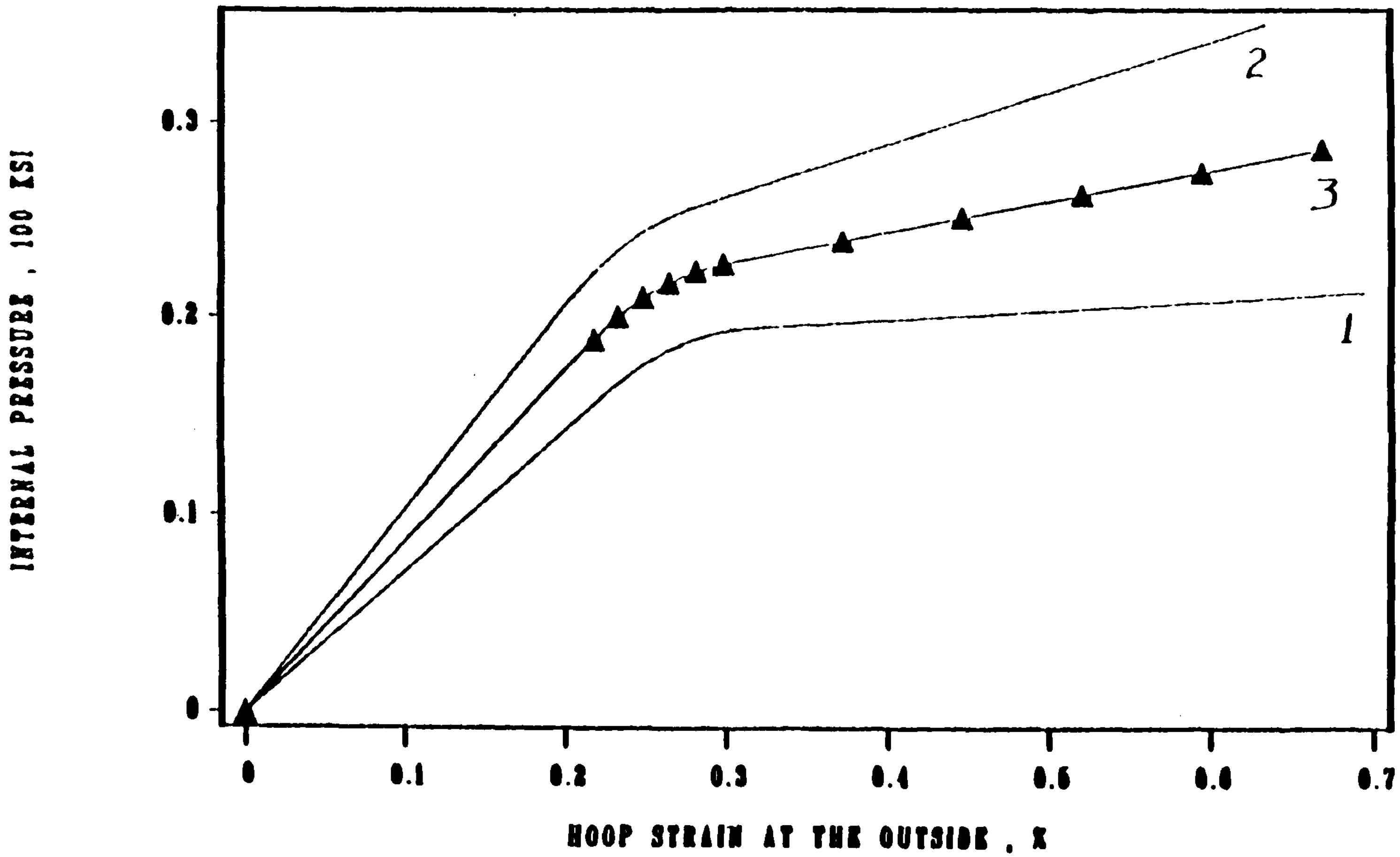

Figure 4. Hoop strain at the outside as a function of internal pressure.

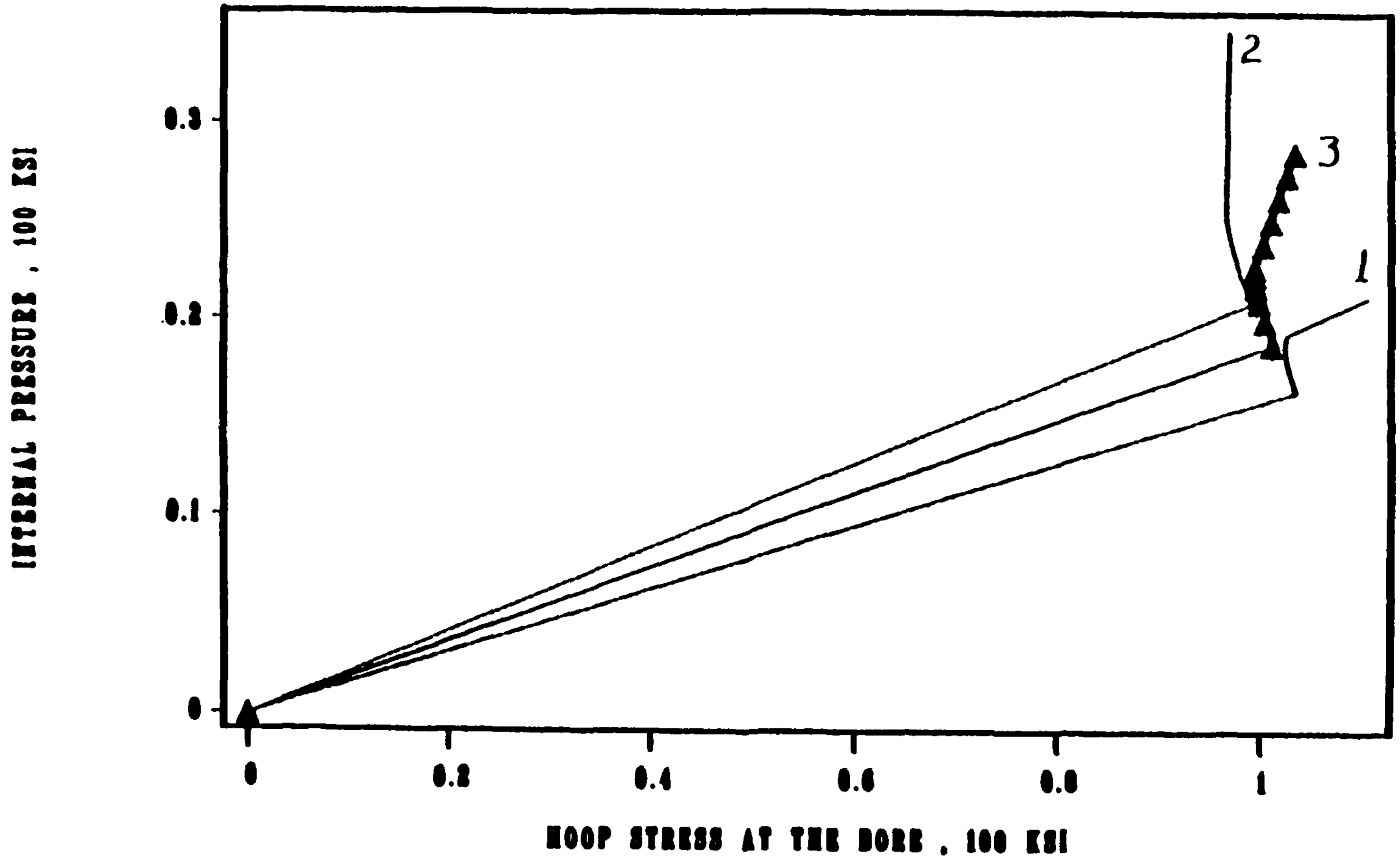

Figure 5. Hoop stress at the bore as a function of internal pressure.

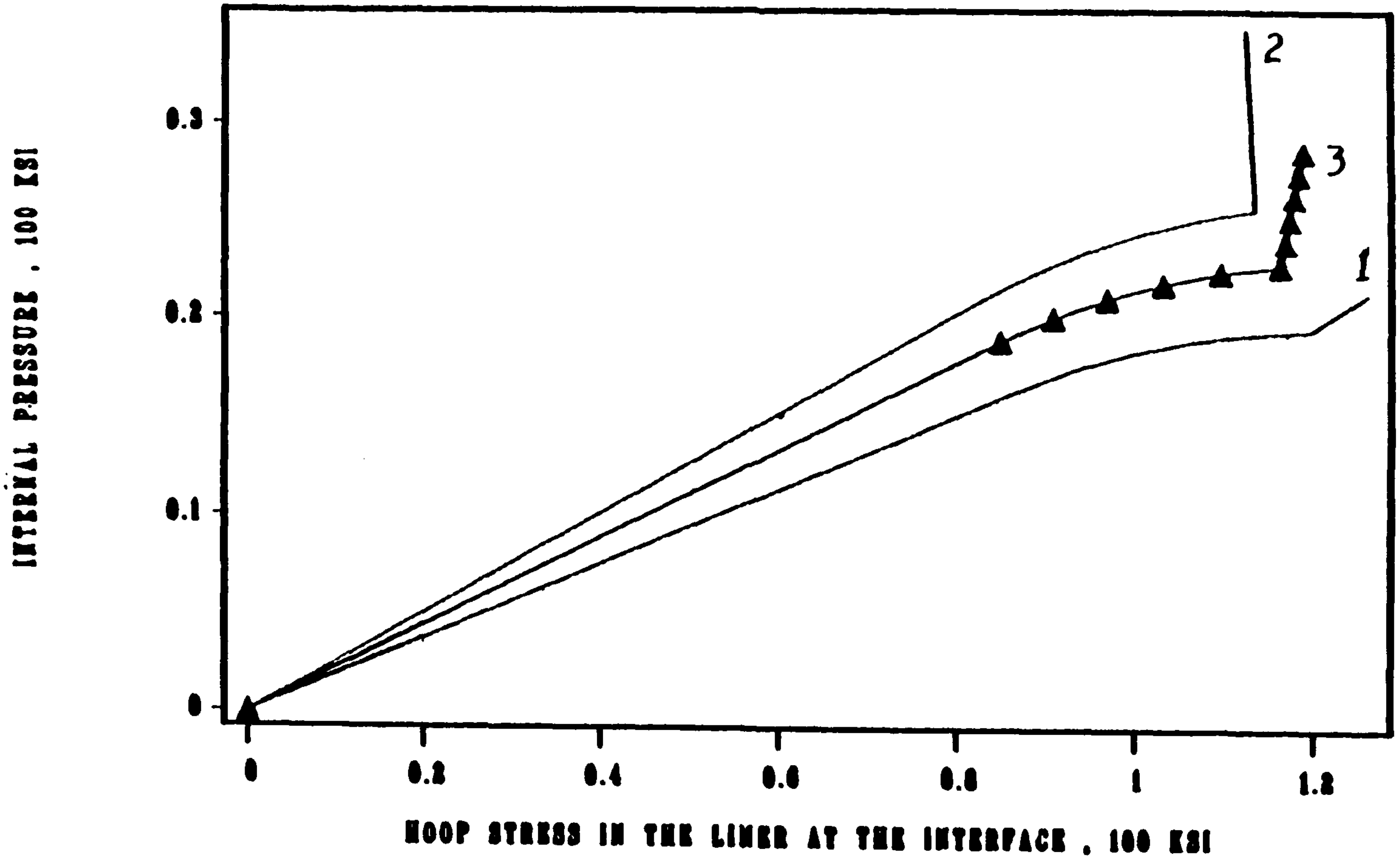

Figure 6. Hoop stress in the liner at the interface as a function of internal pressure.

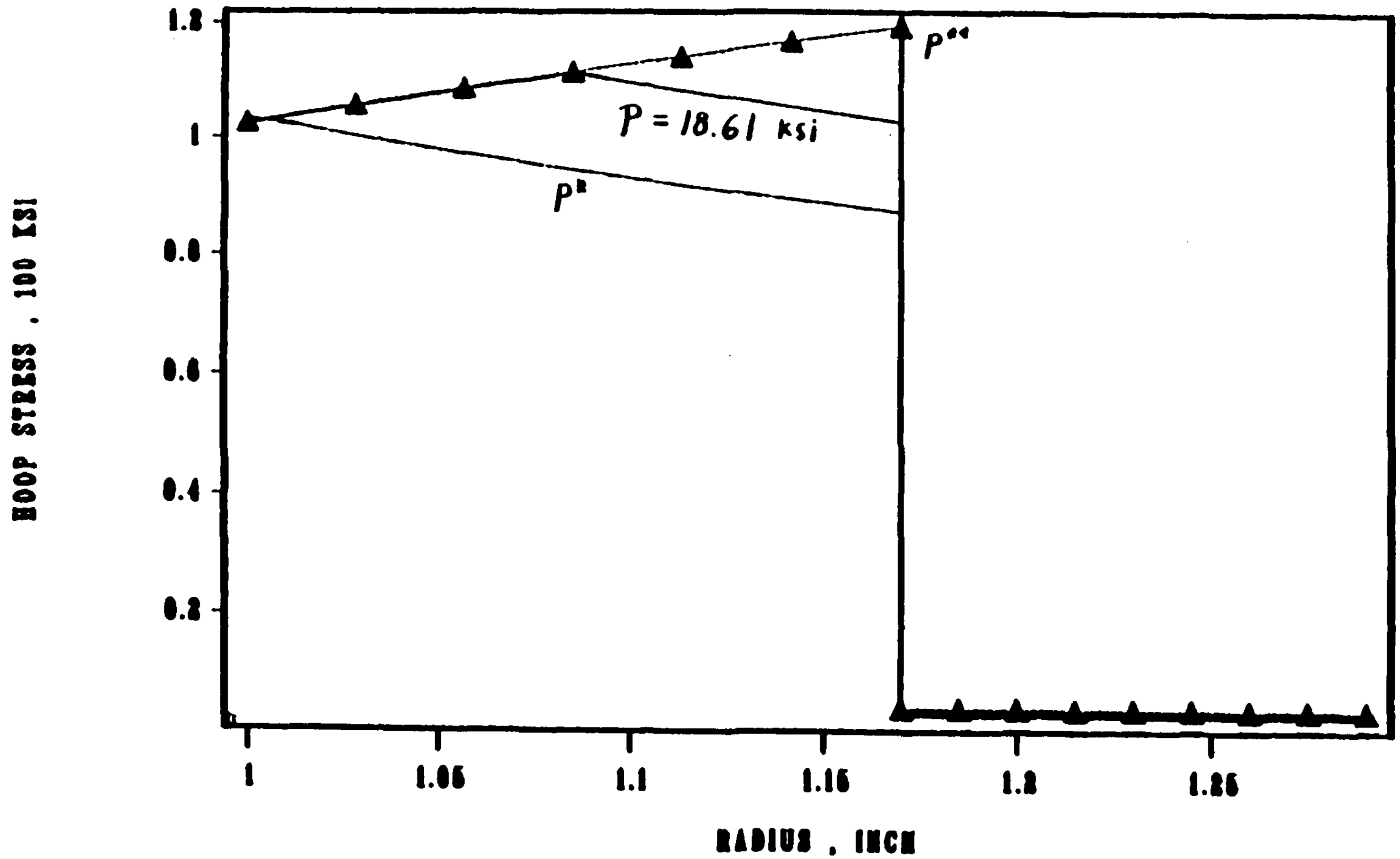

Figure 7. Distribution of hoop stresses in the liner and jacket for case 1.

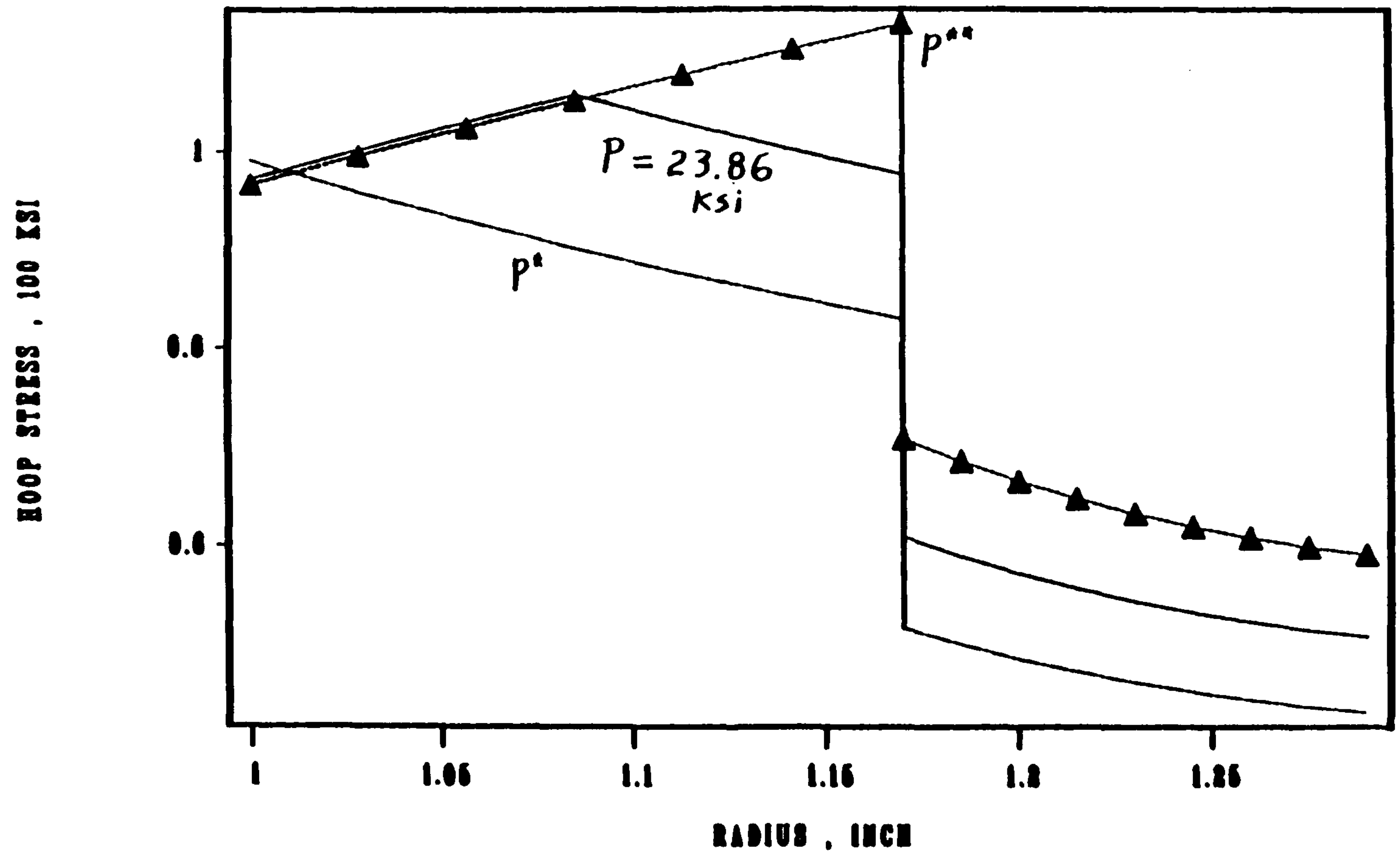

Figure 8. Distribution of hoop stresses in the liner and jacket for case 2.

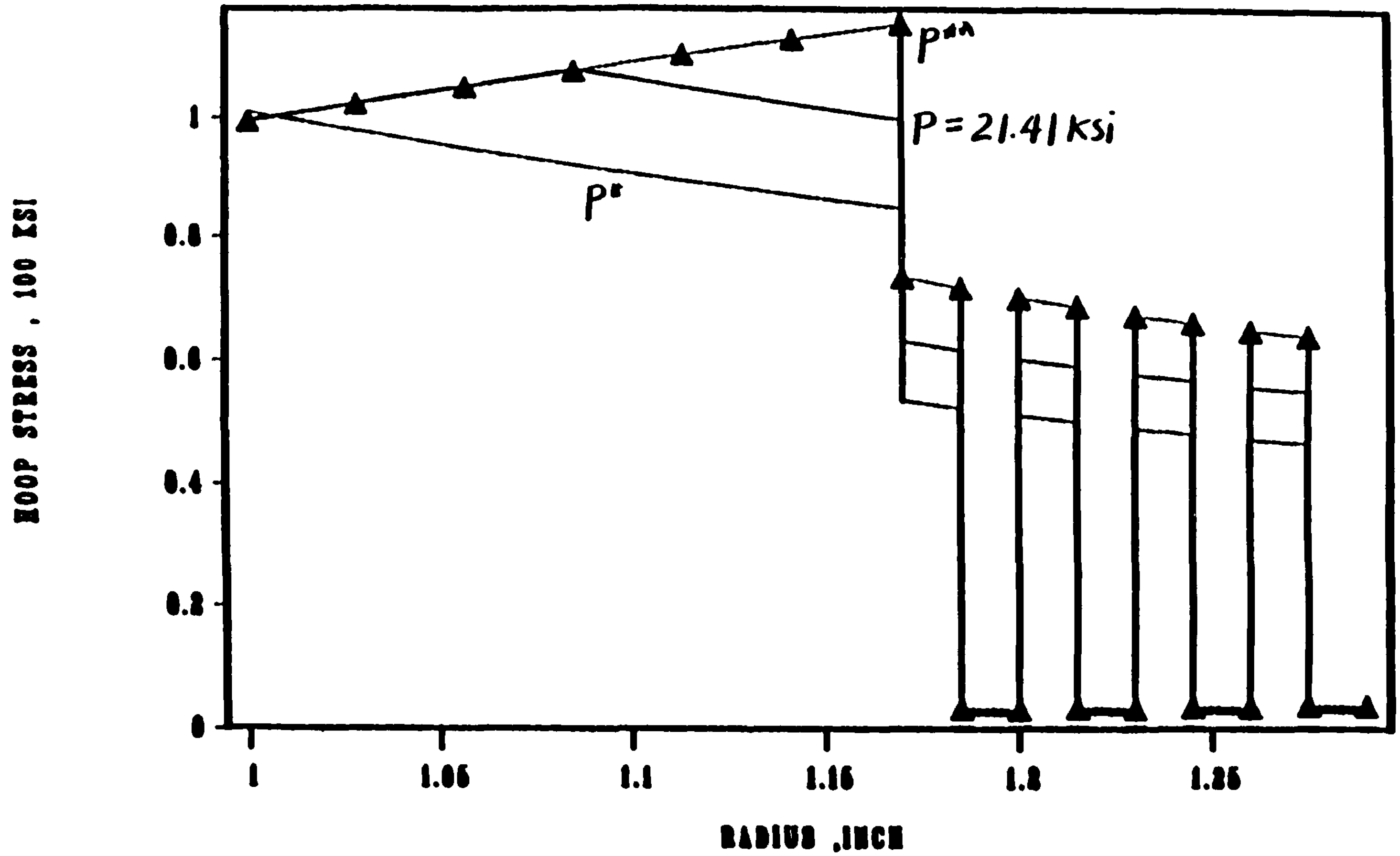

Figure 9. Distribution of hoop stresses in the liner and jacket for case 3.

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